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HIGH-ALTITUDE COOLING

II - AIR-COOLED ENGINES

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SUMMARY

The heat-transfer theory for air-cooled engines is summarized and an analysis of the cooling pressure drop is made for the case in which the pressure drop is an appreciable fraction of the absolute pressure. A chart is given for the simple determination of the cooling pressure drop predicted on the basis of the usual type of sea-level cooling-correlation tests. The method is applied to predict the variation with altitude of the cooling pressure drop required by a typical engine.

INTRODUCTION

In the studies of references 1 to 3 satisfactory semiempirical methods were developed for correlating the cooling-air mass flow with the operating conditions of an air-cooled engine. Because pressure drop is a more practical variable than mass flow and because the product of relative density and pressure drop (or $\sigma\Delta p$) was shown to be determined by the mass flow, this product was used in place of mass flow as the correlation variable. For application in high-altitude flight, however, the mass flow no longer uniquely determines $\sigma\Delta p$ because of compressibility effects, and an extension of the correlation methods is needed if the required cooling pressure is not to be seriously underestimated. Some discussions of the problem have already been given, together with analyses of the effects (reference 4).

The purpose of the present paper is to summarize the theoretical basis for the compressibility correction and to present a chart by means of which the correction can be made simply and with a minimum of supplemental data. The theory of reference 1 is first outlined and the differential equation of the cooling-air flow is then set up in a simplified form and solved. The chart is a plot of the ratio of cooling pressure drop to absolute pressure against a single parameter that can be simply computed from the data.

This paper is the second of the series on high-altitude cooling introduced in reference 5.

SYMBOLS

V velocity

ρ density

σ relative density $(\rho/0.002378)$

q dynamic pressure

p static pressure, absolute

Δp static-pressure drop

T temperature, °F absolute

t temperature, °F

ΔT temperature rise, °F

$G = \rho V$

R gas constant

F pressure-drop coefficient of fin passage

W weight flow of cooling air

M_0 weight flow of engine charge air

a_1 cylinder internal area

a_0 cylinder external area

H heat-transfer rate

L length of fin passage

x distance along fin passage

b fraction of total heat added in front of baffles

γ ratio of specific heats

m', n, m, B, A', K, K' experimental constants

Subscripts:

o free stream

s free-stream stagnation

1 stagnation region in front of engine

2 in baffle entrance

3 in baffle exit

m average between stations 2 and 3

h head

g gas

i incompressible flow

alt at altitude

SL at sea level

av average

BASIC PRINCIPLES OF HEAT TRANSFER

The heat-transfer equations. - In the theory of heat transfer as developed in reference 1, a cylinder wall is considered as a hot body at temperature T_h, heated from within by a flow of hot gases at temperature T_g, and cooled from without by air at temperature T_1. The rate of heat transfer within the cylinder is assumed to vary as the product of the temperature difference $T_g - T_h$ and a heat-transfer coefficient. This internal heat-transfer coefficient varies as some power m of the weight flow of charge air M_e. Thus the heat transferred per second is

$$H = \bar{B}\, M_e^{\,m} a_1 (T_g - T_h) \qquad (1)$$

where

a_1 internal area

$\bar{B}\, M_e^{\,m}$ average internal heat-transfer coefficient

M_e weight flow of charge air

$\bar{B}, m$ experimental constants

On the outside, the heat rejected to the cooling air is assumed to vary as the product of the temperature difference $T_h - T_1$ and some power m' of the weight flow of cooling air W:

$$H = K' W^{m'} a_o (T_h - T_1) \qquad (2)$$

where

a_o external cylinder area

$K' W^{m'}$ average external heat-transfer coefficient

K', m' experimental constants

From equations (1) and (2)

$$\frac{M_e^{\,m/m'}}{W \left(\dfrac{T_h - T_1}{T_g - T_h}\right)^{1/m'}} = A'$$

$$= \left(\frac{K' a_o}{\bar{B} a_1}\right)^{1/m'} \qquad (3a)$$

or

$$W = \frac{M_e^{m/m'}}{A'\left(\frac{T_h - T_l}{T_g - T_h}\right)^{1/m'}} \qquad (3b)$$

This equation is the fundamental relation for the determination of the weight flow of cooling air under any engine operating condition and at any altitude. If the engine operating conditions are constant, the altitude affects the weight flow W required for cooling only insofar as it affects T_l.

Discussion of the variables of equations (3). - The effective gas temperature T_g for a given engine is a function of the operating conditions. This temperature increases, for example, with increase in carburetor-air temperature or with increase in the temperature rise through the blower and thereby affects W and Δp. Some characteristic curves (fig. 1) show that the required cooling pressure drop (roughly proportional to W^2) was nearly doubled when the carburetor inlet-air temperature was increased from 100° F to 300° F. As shown in reference 2, T_g is also sensitive to variations in fuel-air ratio. A plot of the relative sea-level pressure drop required to cool a certain engine at constant indicated horsepower against fuel-air ratio (fig. 2) shows that increasing the fuel-air ratio from 0.08 to 0.10 reduced the required cooling pressure drop by half.

Figure 2 shows that the use of very lean mixtures likewise reduces the required cooling pressure drop. This cooling aid, which might be particularly applicable in the cruising range, is not easily used, however, because, without exact methods of fuel metering and without perfect uniformity of the mixture distribution to the different cylinders, the use of fuel-air ratios so close to the cut-out point becomes dangerous.

The symbol T_h of equations (3) was originally defined as a mean cylinder-head temperature; however, satisfactory correlations of multicylinder engines have been made with T_h taken as the mean rear spark-plug-gasket temperature. It is probable that other points could be used equally well; the values of exponents m and m' would depend on the point chosen.

The temperature T_1 is the external air temperature corrected for the adiabatic temperature rise resulting from the airplane speed:

$$T_1 = T_0 + 0.83\left(\frac{V_0}{100}\right)^2 \qquad (4)$$

where the temperatures are in °F absolute and V_0 is in feet per second.

Values for T_g, A', m, and m' are found by analysis of systematic sea-level test data according to the methods of reference 1. The exponents m and m' are both generally about 2/3. The value of T_g for cylinder heads is normally of the order of 1610° F absolute (1150° F) when the intake manifold temperature is 80° F and the fuel-air ratio is 0.08; however, as already indicated, this value is subject to considerable variation. It is affected, for example, by changes in back pressure; sea-level tests showed a decrease of 20 percent in the required cooling pressure drop when the exhaust manifold pressure was halved. This effect is of little importance when a turbosupercharger is used because the back pressure will be of the order of sea-level pressure in the region of the critical altitude.

As already remarked, the main effect of altitude on the required weight flow of cooling air results simply from the variation of T_1 with altitude; that is, for otherwise constant conditions,

$$\frac{W}{W_{SL}} = \frac{{(T_h - T_1)_{SL}}^{1/m'}}{(T_h - T_1)^{1/m'}}$$

where the subscript SL refers to sea-level conditions. Figure 3, based on this equation, shows the variation of W with altitude for several values of T_h. The temperature of Army air, uncorrected for flight speed, was used for T_1, and m' was taken as 2/3. The reduction in required weight flow with increasing altitude is seen to be most pronounced when T_h is low. Because cylinder barrels are about 150° F cooler than cylinder heads, the barrels will be expected to overcool at altitude if sufficient cooling pressure drop is maintained for constant head temperature. Actually, the thermal problems of heads and barrels are not quite independent because heat is conducted between them.

In sea-level tests the pressure drop of the cooling air has been shown experimentally to vary as a constant power of its weight flow W:

$$\sigma\Delta p \propto W^{m'/n}$$

where m'/n is very nearly 2. Because the pressure drops Δp is more easily measured than W and is also of more direct interest in practice, $\sigma\Delta p$ has until now replaced W as a variable in cooling-correlation tests at sea level. A possible form of equation (3) is thus

$$\frac{T_h - T_l}{T_g - T_h} = K \frac{F_e^{\,m}}{(\sigma\Delta p)^n} \tag{3c}$$

The modification of this expression necessary for cooling correlation at high altitudes will be next considered.

PRESSURE DROP

Air-flow path. - The assumed path of the cooling air through the engine is shown diagrammatically in figure 4, together with the static-pressure drops in the different parts of the path. The air accelerates from the stagnation region 1 in front of the cylinder into the baffle entrance 2; the static pressure falls by an amount Δp_{1-2}. The air then flows along the fin passages, which are assumed to be of constant cross section. The static-pressure drop Δp_{2-3} that occurs along the passages is the sum of the friction pressure drop and the pressure drop necessary to accelerate the air as its density decreases along the passages. The air finally flows from the baffle exit into the space behind the cylinder. The abrupt enlargement of the flow passages causes a dissipation of most of the kinetic energy at the baffle exit; that is, the flow into the space behind the engine occurs at essentially constant static pressure. The pressure drop across the engine is thus simply the sum of Δp_{1-2} and Δp_{2-3}.

Pressure drop at the baffle inlet. - At low altitudes, for which the entrance Mach numbers are low, the pressure drop Δp_{1-2} is $\frac{1}{2}\rho_2 V_2^2$. For higher entrance Mach numbers, the pressure drop into the baffles is given by the formula for compressible flow. It is assumed that the fraction b of the total heat input occurs without pressure loss in the stagnation region in front of the cylinder; that is, just before the air accelerates into the baffles, its temperature is $T_1 + b\Delta T$, where T_1 is given by equation (4) and ΔT is determined from the heat rejection and the weight flow. The density here is $\rho_1\left(\frac{T_1}{T_1 + b\Delta T}\right)$

If the flow into the baffles is isentropic, then, by the Bernoulli equation,

$$V_2^2 = \frac{2\gamma}{\gamma - 1}\frac{p_1}{\rho_1}\frac{T_1 + b\Delta T}{T_1}\left[1 - \left(\frac{p_2}{p_1}\right)^{\frac{\gamma-1}{\gamma}}\right] \tag{5}$$

or

$$\frac{G^2\left(1 + b\frac{\Delta T}{T_1}\right)}{p_1\rho_1} = \frac{2\gamma}{\gamma - 1}\left[1 - \left(\frac{p_2}{p_1}\right)^{\frac{\gamma-1}{\gamma}}\right]\left(\frac{p_2}{p_1}\right)^{2/\gamma} \tag{6}$$

where $G = \rho_2 V_2 = \rho V$ along the passage and is given by the mass flow and the flow area of the baffles. The ratio p_2/p_1 is thus given as a function of the term on the left side of this equation; a plot of p_2/p_1 against $\frac{G^2\left(1 + b\frac{\Delta T}{T_1}\right)}{p_1\rho_1}$ is given in figure 5.

For convenience in the use of equation (6), figures 6 to 8 show T_1, p_1, and ρ_1 for Army air plotted against altitude for several airplane speeds. It was assumed that only 90 percent of the flight dynamic pressure is recovered as static pressure; that is,

$$p_1 - p_0 = 0.9\ (p_s - p_0)$$

where

p_s free-stream stagnation pressure

p_0 free-stream static pressure

Pressure drop within the baffles. - The pressure decreases along the fin passages as a result of friction and acceleration:

$$-dp = \frac{1}{2}\ \rho V^2\ \frac{F}{L}\ dx + \ \rho V\ dV$$

where

x distance along passage

L length of passage

F friction coefficient for passage as a whole (that is, $\frac{1}{2}\rho V^2 F$ would be the pressure drop within the fin passages in absence of temperature and acceleration effects)

This equation is, for convenience, rewritten as follows:

$$-\rho\ \ dp = G^2\left(\frac{F}{2L}\ \frac{dx}{dT}\ dT + \frac{dV}{V}\right) \qquad (7)$$

In order to simplify the solution, heat is assumed to be rejected uniformly to the cooling air along the passage:

$$\frac{dx}{dT} = \frac{L}{T_3 - T_2} = \frac{L}{(1 - b)\Delta T}$$

From the gas law,

$$p = \rho RT$$

and from the condition that ρV is a constant (equal to G),

$$\frac{dV}{V} = -\frac{d\rho}{\rho} = \frac{dT}{T} - \frac{dp}{p}$$

These two substitutions reduce equation (7) to

$$dp\left(\frac{T}{p} - \frac{p}{RG^2}\right) = dT\left[\frac{FT}{2(1 - b)\Delta T} + 1\right] \qquad (8)$$

The friction factor F, which is a function of the Reynolds number, may be evaluated for the mean viscosity along the passage and treated as a constant. As a further simplification, the first term $\frac{T}{p}\,dp$ is integrated by considering T constant at its mean value $\frac{T_3 + T_2}{2} = T_m$. The temperature T_2 is given by

$$\frac{T_2}{T_1 + b\Delta T} = \left(\frac{p_2}{p_1}\right)^{\frac{\gamma - 1}{\gamma}} \qquad (9)$$

The ratio $\frac{T_2}{T_1 + b\Delta T}$ is shown in figure 5 as a function of

$$\frac{G^2\left(1 + b\frac{\Delta T}{T_1}\right)}{p_1\rho_1}$$

Also

$$T_3 = T_2 + (1 - b)\Delta T$$

Integration of equation (8) between stations 2 and 3 gives

$$\log\left(\frac{p_3}{p_2}\right)^2 + \frac{p_2{}^2}{RG^2T_m}\left[1 - \left(\frac{p_3}{p_2}\right)^2\right] \cdot \left[F + \frac{2(T_3 - T_2)}{T_m}\right] = 0 \quad (10)$$

The ratio p_3/p_2 is thus determined by the two dimensionless groups $\frac{p_2{}^2}{RG^2T_m}$ and $F + \frac{2(T_3 - T_2)}{T_m}$; this ratio is plotted as a function of these two quantities in figure 9.

The pressure drop follows from the values of p_2/p_1 and p_3/p_2 determined by equations (6) and (10):

$$p_1 - p_3 = p_1\left(1 - \frac{p_2}{p_1}\frac{p_3}{p_2}\right) \quad (11)$$

In applying the theory when b, p_1, and T_1 are given, the three quantities G, ΔT, and F must be known. As an illustration of the process of computing $p_3 - p_1$, suppose $p_1 = 582$ pounds per square foot, $T_1 = 456^\circ$ F absolute, and $\rho_1 = 0.000744$ slug per cubic foot, with $G = 0.2172$ slug per foot^2 second, $\Delta T = 123^\circ$ F, $F = 1$, and $b = 0.5$.

First compute

$$\frac{G^2}{p_1\rho_1}\left(1 + \frac{\Delta T}{2T_1}\right) = \frac{(0.2172)^2}{582 \times 0.000744}\left(1 + \frac{123}{2 \times 456}\right)$$

$$= 0.1089\left(1 + \frac{0.27}{2}\right)$$

$$= 0.1236$$

From figure 5(a), $p_2/p_1 = 0.9338$ and $\frac{T_2}{T_1 + \frac{\Delta T}{2}} = 0.980$

Now,

$$T_m = T_2 + \frac{1}{4}\Delta T$$

$$= 0.980\left(T_1 + \frac{\Delta T}{2}\right) + \frac{1}{4}\Delta T$$

$$= 0.980\ (456 + 61.5) + 30.8$$

$$= 537.4^{\circ}\ \text{F absolute}$$

Then

$$F + 2\left(\frac{T_3 - T_2}{T_m}\right) = 1 + \frac{123}{537.4}$$

$$= 1.23$$

Also

$$\frac{p_2{}^2}{RG^2T_m} = \frac{p_2{}^2}{p_1{}^2}\ \frac{p_1\rho_1}{G^2}\ \frac{T_1}{T_m}$$

$$= (0.9338)^2 \times \frac{1}{0.1089} \times \frac{456}{537.4}$$

$$= 6.795$$

From figure 9, therefore,

$$\frac{p_3}{p_2} = 0.885$$

and

$$p_1 - p_3 = 582(1 - 0.9338 \times 0.885$$

$$= 101.2 \text{ pounds per square foot}$$

$$= 19.5 \text{ inches of water}$$

Simplified chart for pressure-drop determination. - The number of independent variables required to fix Δp was found in the computation to be five excluding b, but this number can be reduced. The ratio

$$\frac{\Delta p}{p_1} = \frac{p_1 - p_3}{p_1}$$

$$= 1 - \frac{p_2}{p_1}\frac{p_3}{p_2} \qquad (12)$$

is completely determined by three dimensionless quantities $G^2/p_1\rho_1$, $\Delta T/T_1$, and F, if b is known. It will be assumed for the remaining development that $b = \frac{1}{2}$. The value of p_2/p_1 is determined by use of equation (6) from the variable

$$\frac{G^2}{p_1\rho_1}\left(1 + \frac{1}{2}\frac{\Delta T}{T_1}\right)$$

Likewise, p_3/p_2 is found by use of equation (10) from the two variables

$$\frac{{p_2}^2}{RG^2T_m} = \frac{{p_2}^2}{{p_1}^2}\frac{p_1\rho_1}{G^2}\frac{T_1}{T_m} \qquad (13)$$

and

$$F + \frac{\Delta T}{T_m}$$

The factor p_2/p_1 of equation (13) is determined by $G^2/p_1\rho_1$ and $\Delta T/T_1$. The factor

$$\frac{T_1}{T_m} = \frac{T_1}{\left(T_1 + \frac{\Delta T}{2}\right)\left(\frac{p_2}{p_1}\right)^{\frac{\gamma-1}{\gamma}} + \frac{1}{4}\Delta T}$$

$$= \frac{1}{\left(1 + \frac{1}{2}\frac{\Delta T}{T_1}\right)\left(\frac{p_2}{p_1}\right)^{\frac{\gamma-1}{\gamma}} + \frac{1}{4}\frac{\Delta T}{T_1}} \tag{14}$$

is obviously determined by the same variables. Also

$$\frac{\Delta T}{T_m} = \frac{\Delta T}{T_1}\frac{T_1}{T_m}$$

so that p_3/p_1, p_2/p_1, and hence $\Delta p/p_1$ are known when $G^2/p_1\rho_1$, $\Delta T/T_1$, and F are known, as was to be proved.

If the number of variables required to find $\Delta p/p_1$ could not be reduced to less than three, an obvious way to simplify the computation of $\Delta p/p_1$ would be to plot it against one of the three dimensionless variables, say $G^2/p_1\rho_1$, for various values of the other two, $\Delta T/T_1$ and F. The use of such a system of curves would be awkward in practice because it would generally require a double interpolation in finding $\Delta p/p_1$. It is therefore proposed to find a variable to replace $G^2/p_1\rho_1$ such that curves of $\Delta p/p_1$ against the variable will be as nearly as possible coincident for various values of $\Delta T/T_1$ and F; that is, an attempt will be made to express $\Delta p/p_1$ in terms of one variable instead of three.

In its passage through the baffle passages, the cooling air experiences expansion due to heating and pressure drop. At very high total pressures the expansion due to pressure drop is negligible; the cooling pressure drop under such circumstances can be shown to be very roughly

$$\frac{G^2}{2\rho_1}\left(1 + \frac{\Delta T}{2T_1}\right)\left(1 + F + \frac{\Delta T}{T_1}\right)$$

When $\Delta p/p_1$ is plotted against the variable

$$\frac{G^2}{2\,p_1\rho_1}\left(1 + \frac{\Delta T}{2T_1}\right)\left(1 + F + \frac{\Delta T}{T_1}\right)$$

for various values of $\Delta T/T_1$ and F, the resultant curves as shown in figure 10 are nearly coincident for small values of $\Delta p/p_1$. The curves represent the effect of expansion due to pressure changes alone. In practice, these curves can be used in place of the previously described calculation to compute Δp. The curves as computed are rigorously correct and are not affected by the approximation used in choosing the form of the variable

$$\frac{G^2}{2\,p_1\rho_1}\left(1 + \frac{\Delta T}{2T_1}\right)\left(1 + F + \frac{\Delta T}{T_1}\right)$$

The only error in the use of these curves comes from the approximation required in interpolating for different values of F and $\Delta T/T_1$. Because of the small separation of the various curves, the errors due to interpolation are negligible compared with experimental errors in measuring pressure drops.

Application of theory to experiment. - It is apparent that certain changes from present practice in testing procedure are required if cooling pressure drops at altitude are to be predicted. In place of the quantity $\sigma\Delta p$, a true mass-flow index such as G must be observed and used in the correlation; also ΔT and F, which have not ordinarily been used, must be measured.

A more explicit description of a possible procedure is as follows:

A mass-flow index $(\sigma\Delta p)_i$ is defined by

$$(\sigma\Delta p)_i = \frac{G^2}{2\rho_o}(1 + F) \tag{15}$$

where ρ_o is the sea-level density, 0.002378 slug per cubic foot. The pressure drop Δp in $(\sigma\Delta p)_i$ might be observed across a cold engine at sea level; $(\sigma\Delta p)_i$ is a true mass-flow index and varies for a particular engine only with G. The variable

$$\frac{G^2}{2\,p_1\rho_1}\left(1 + \frac{\Delta T}{2T_1}\right)\left(1 + F + \frac{\Delta T}{T_1}\right)$$

is then written

$$\frac{(\sigma\Delta p)_i}{\sigma_1 p_1}\left(1 + \frac{\Delta T}{2T_1}\right)\left[1 + \frac{\Delta T}{T_1(1 + F)}\right]$$

A curve of F against $(\sigma\Delta p)_i$ is first made by use of equation (15) and the values of G and $\sigma\Delta p$ measured on a cold engine. The usual type of correlation tests are then made but, in place of $\sigma\Delta p$, the new variable $(\sigma\Delta p)_i$ is used to fix the curve of

$\dfrac{T_h - T_l}{T_g - T_h}$ against $\dfrac{M_e^{\,m/n}}{(\sigma\Delta p)_i}$. The variable $(\sigma\Delta p)_i$ is found from figure 10 and the observed value of $\Delta p/p_1$, observed values of $\Delta T/T_1$, and F estimated by use of the curve of F against $(\sigma\Delta p)_i$

Finally, an experimental curve of $\Delta T/(T_h - T_1)$ against $(\sigma\Delta p)_i$ is made from these values of $(\sigma\Delta p)_i$ and the corresponding measured temperatures.

In order to predict a cooling pressure drop, the required value of $(\sigma\Delta p)_i$ is found from the curve of $\frac{T_h - T_1}{T_g - T_h}$ against $\frac{{M_e}^{m/n}}{(\sigma\Delta p)_i}$ in the customary way. The values of ΔT and F are found from the proper curves against $(\sigma\Delta p)_i$, and the variable

$$\frac{(\sigma\Delta p)_i}{\sigma_1 p_1}\left(1 + \frac{\Delta T}{2T_1}\right)\left[1 + \frac{\Delta T}{T_1(1 + F)}\right]$$

is computed. The ratio $\Delta p/p_1$, and hence Δp, is found by use of figure 10.

In figure 11 the relations of figure 10 are plotted in slightly different form. The ordinate is the same but the abscissa is $(\sigma\Delta p)_i/\sigma_1 p_1$.

It is of interest to observe that a correlation curve of the conventional type based on $\sigma_3\Delta p$, where σ_3 is the density ratio of the air at the baffle outlet, will predict the required cooling pressure drops within about 5 percent at all altitudes. Because σ_3 is not easily predicted in practice, the theory as derived is considered preferable for cooling correlation.

Estimation of required cooling pressure drop from conventional correlation data. - If no complete cooling data are available, a rough estimate of the required cooling pressure drop at altitude can be made by use of a conventional cooling correlation.

The solid line of figure 12 is such a curve for a modern engine; it is a plot of $\frac{(T_h - T_1)}{(T_g - T_h)}$ as a function of the variable ${M_e}^{1.76}/\sigma\Delta p$. The assumption is now made that a similar curve with the variable ${M_e}^{1.76}/(\sigma\Delta p)_i$

would be parallel to, but displaced from, the curve shown; hence, only a single value of $(\sigma\Delta p)_i$ will be sufficient to fix the new correlation curve. If $\Delta T/T_1$ and F are known at one engine condition with the corresponding value of $\Delta p/p_1$, the value of $(\sigma\Delta p)_i$ can be found by use of figure 10 and the point and curve can be located. In predicting cooling pressure drops at altitude, $(\sigma\Delta p)_i$ is found from the assumed new correlation curve and F is assumed constant. The term ΔT is computed from the data of the one test in which ΔT was measured by use of the relation

$$\Delta T \propto (T_h - T_1)(\sigma\Delta p)_i^{\,n-\frac{1}{2}}$$

where n is the slope of the correlation curve. Either figure 10 or figure 11 may then be used to find the cooling pressure drop at altitude.

In case only single-cylinder correlation data are available and the cooling of a multicylinder engine is required to be known, it should be remembered that some spread in temperature is expected among the engine cylinders. In order that no cylinder shall overheat, all but one of them must be overcooled. The spread between the hottest and the coldest cylinders is expected to be as much as 100° F for a modern double-row engine.

Example. - Let it be required to estimate the pressure drop to cool the engine to which figure 12 pertains under the following conditions:

Output, horsepower	2000
Altitude, feet	35,000
Airplane speed, miles per hour	350
Maximum head temperature, °F	500
Fuel-air ratio	0.105
Carburetor-air temperature, °F	100

In one test at sea level $\sigma_1\Delta p$ was found to be 13.8 inches of water for $t_h = 355°$ F, $t_1 = 81°$ F, and

$\frac{T_h - T_1}{T_g - T_h} = 0.470$. The temperature rise observed was 61° F, σ_1 was 0.940, and p_1 was 398.7 inches of water; F was found to be approximately equal to 1.

From figure 10 with

$$\frac{\Delta p}{p_1} = \frac{13.8}{0.94 \times 398.7}$$

$$= 0.0368$$

$$\frac{(\sigma\Delta p)_1}{\sigma_1 p_1}\left(1 + \frac{\Delta T}{2T_1}\right)\left(1 + \frac{\Delta T}{T_1}\frac{1}{1 + F}\right) = 0.0353$$

Therefore

$$(\sigma\Delta p)_1 = \frac{0.0353 \times 0.94 \times 398.7}{\left(1 + \frac{61}{541 \times 2}\right)\left(1 + \frac{61}{2 \times 541}\right)}$$

$$= 11.9 \text{ inches of water}$$

and

$$\frac{M_e^{1.76}/\sigma\Delta p}{M_e^{1.76}/(\sigma\Delta p)_1} = \frac{11.9}{13.8}$$

$$= 0.862$$

The curve of $M_e^{1.76}/(\sigma\Delta p)_1$ as assumed is shown dotted in figure 12; each abscissa is increased in the ratio 1:0.862 above its value on the solid curve.

For the assumed conditions, assume further $t_h = 450^\circ$ F or 50° F below the limiting head temperature of the hottest cylinder. From figure 6, $T_l = 456^\circ$ F absolute or $t_l = -4^\circ$ F, for the given fuel-air ratio and carburetor-air temperature, $t_g = 1054^\circ$ F, as found from the same correlation tests. Then

$$\frac{T_h - T_l}{T_g - T_h} = \frac{t_h - t_l}{t_g - t_h}$$

$$= 0.751$$

and

$$\frac{M_e^{1.76}}{(\sigma\Delta p)_i} = 3.34$$

From the engine calibration

$$M_e = 4.25 \text{ pounds per second}$$

so that

$$(\sigma\Delta p)_i = \frac{(4.25)^{1.76}}{3.34}$$

$$= 3.82 \text{ inches of water}$$

Since, for $(\sigma\Delta p)_i = 11.9$ inches of water, $\Delta T = 61^\circ$ F, and since the slope of the correlation curve is 0.321,

$$\Delta T = 61 \times \left(\frac{3.82}{11.9}\right)^{0.321-\frac{1}{2}} \times \frac{450 + 4}{355 - 81}$$

$$= 123.7^\circ \text{ F}$$

From figures 7 and 8

$$p_l = 582 \text{ pounds per square foot}$$

$$= 112 \text{ inches of water}$$

$$\rho_1 = 0.000744 \text{ slug per cubic foot}$$

or

$$\sigma_1 = 0.3128$$

Hence

$$\frac{(\sigma\Delta p)_1}{\sigma_1 p_1}\left(1 + \frac{\Delta T}{2T_1}\right)\left(1 + \frac{\Delta T}{T_1}\,\frac{1}{1 + F}\right)$$

$$= \frac{3.82}{0.3128 \times 112}\left(1 + \frac{123.7}{2 \times 456}\right)\left(1 + \frac{123.7}{456} \times \frac{1}{1 + 1}\right)$$

$$= 0.1405$$

By use of figure 10, $\Delta p = \dfrac{0.175 \times 582}{5.2} = 19.6$ inches of water, which is in satisfactory agreement with the example previously computed.

In order to illustrate the altitude effect, the calculations were carried out for a range of altitude up to 50,000 feet with $t_g = 1054^\circ$ F and $t_h = 450^\circ$ F; F was assumed constant in the absence of complete data. In order to show the effects of fuel-air ratio and of the permissible head temperature, calculations were also made for $t_h = 450^\circ$ F and $t_g = 956^\circ$ F, corresponding to a fuel-air ratio of 0.115 for this engine, and for $t_h = 400^\circ$ F (with $t_g = 1054^\circ$ F). The results are plotted in figure 13. It is seen that lowering the permissible value of t_h only 50° F doubled the required pressure drop, whereas increasing the fuel-air ratio 9.5 percent nearly halved the required pressure drop.

The dotted curve in figure 13 represents the dynamic pressure for an airplane speed of 350 miles per hour. If this curve indicates roughly the pressure available for cooling the engine, it is clear that 2000 horsepower could normally be obtained on this engine at altitudes above 40,000 feet only by using 500° F as the limiting head temperature of the average cylinder and enriching the fuel-air mixture more than is usual with such engines.

The curves of figure 13 show the advantage of reducing the spread in cylinder-head temperatures and in fuel-air ratio in cases for which engine cooling at high altitudes must be improved.

CONCLUSIONS

1. For accurate presentation of cooling data over the entire altitude range, it is recommended that cooling-air mass flow or some equivalent variable be used instead of pressure drop in the correlation equation.

2. The simple approximate relation between mass flow and pressure drop that applies at sea level is inaccurate at high altitude. Accurate computation of the pressure drop at high altitude requires a knowledge of the temperature rise of the cooling air and the friction coefficient of the fins in addition to the usual information presented for low-altitude cooling.

3. A curve is shown which greatly simplifies the prediction of altitude cooling pressure drop from the correlation data.

4. A method is presented for adapting present incomplete correlation data to predict altitude cooling requirements. Calculations for a modern engine show that the acceleration of the cooling air because of decreased pressure at 35,000 feet causes nearly a 25 percent increase in pressure drop required for cooling.

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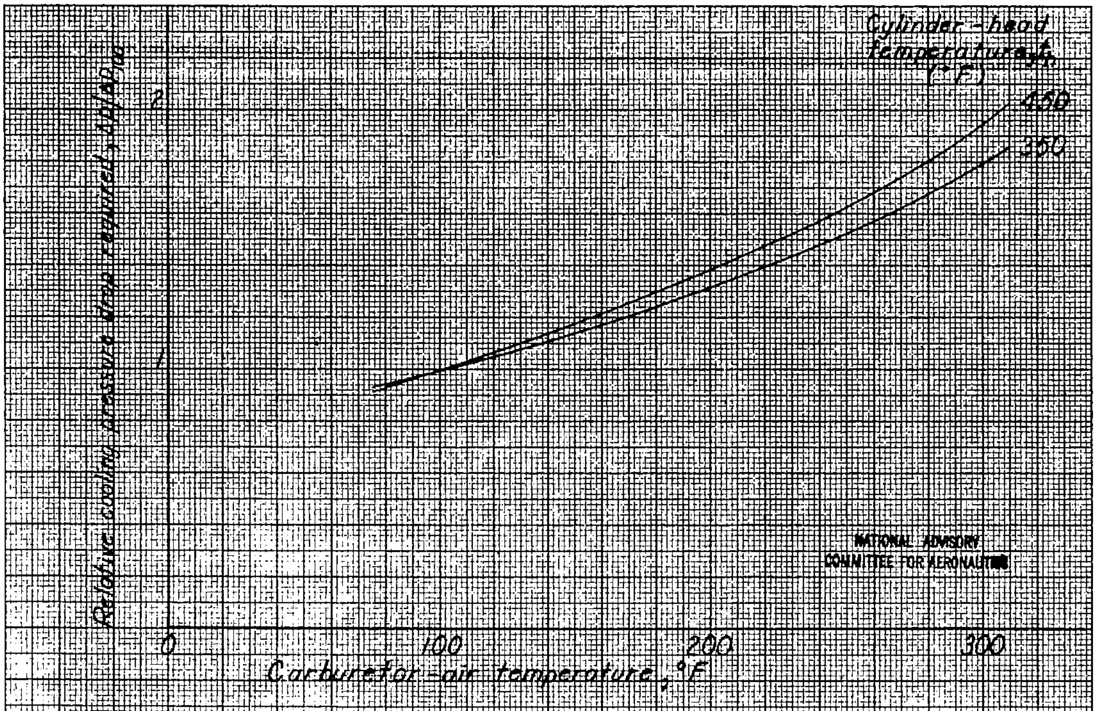


Figure 1.- Effect of carburetor-air temperature on the required cooling pressure drop. Values given relative to pressure drop required for carburetor-air temperature of 100 °F.

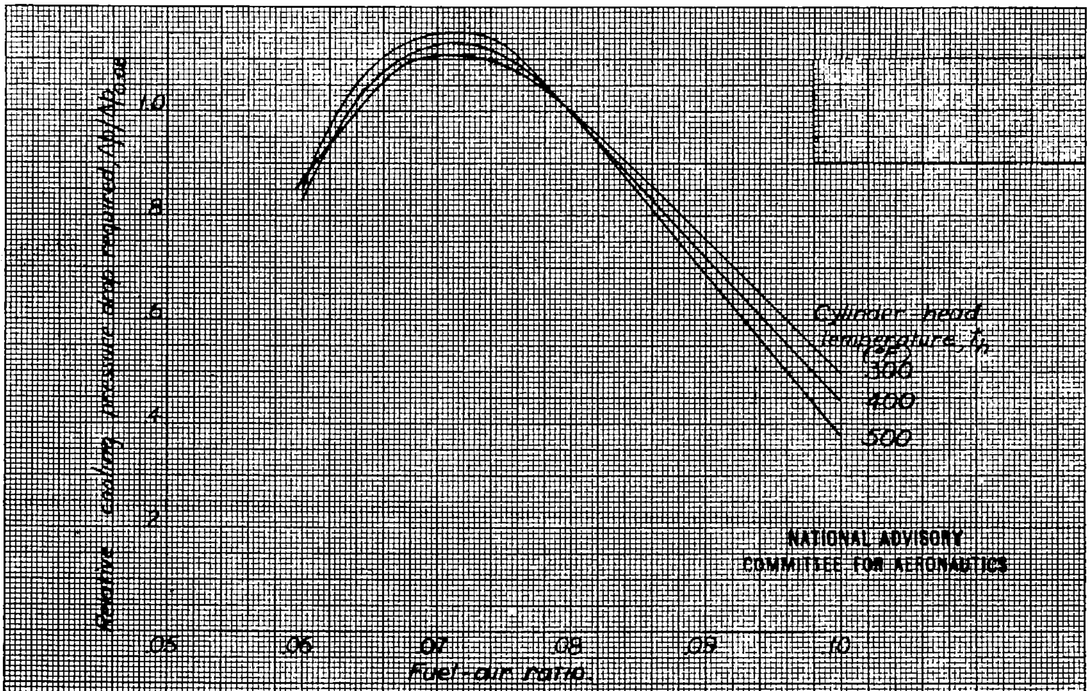


Figure 2.- Effect of fuel-air ratio on sea-level cooling pressure drop. Values are given relative to pressure drop required for fuel-air ratio of 0.08.

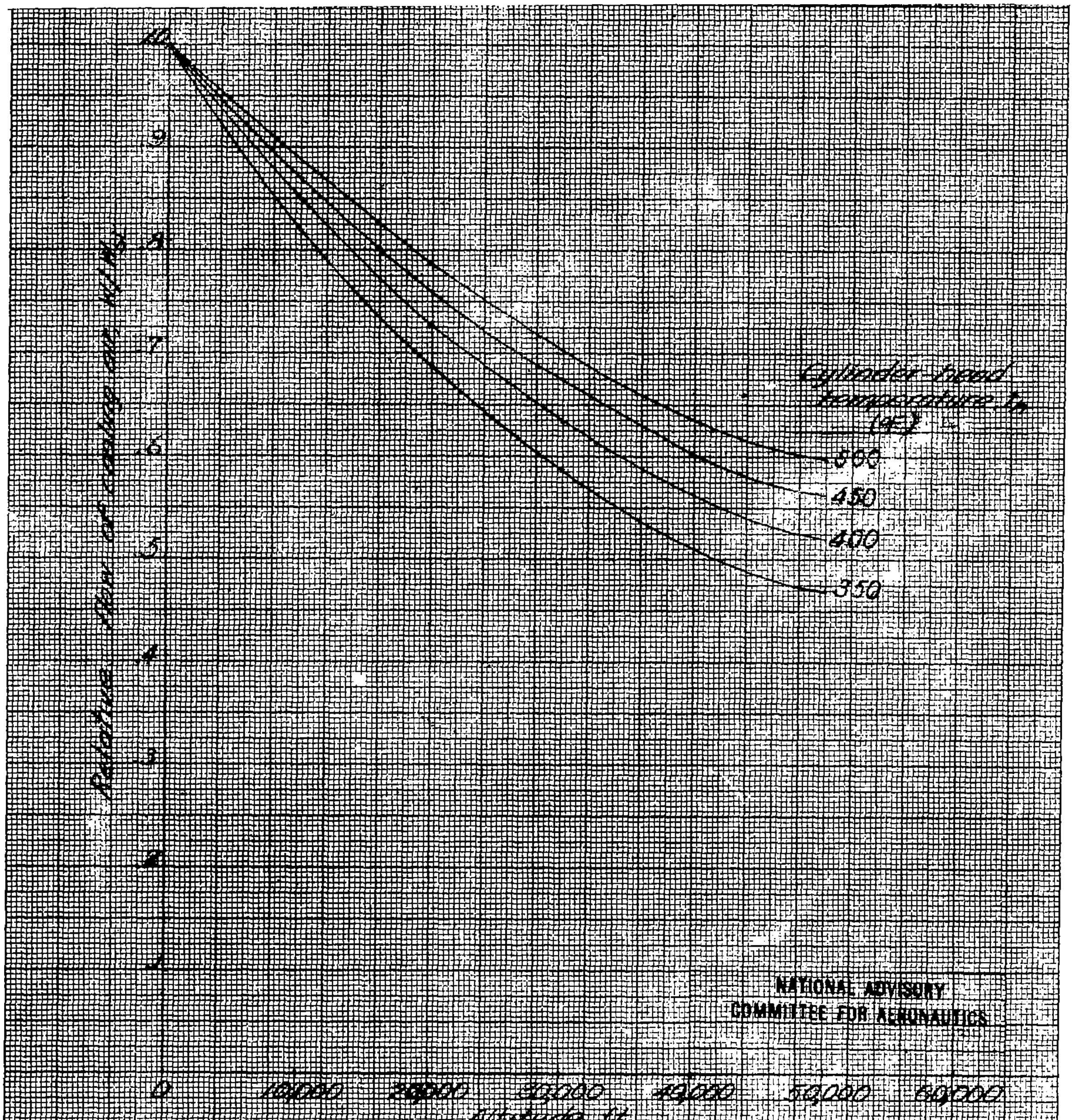
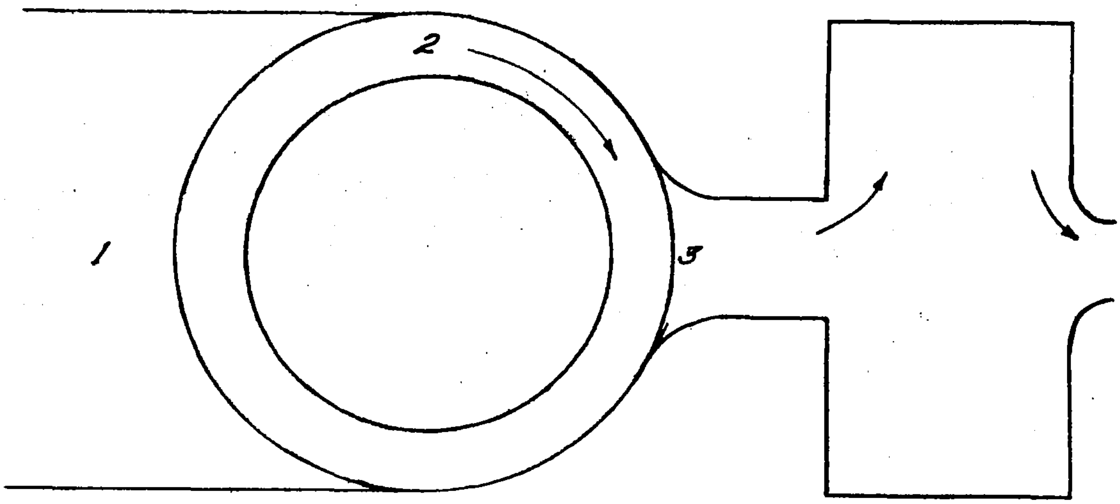


Figure 3.- Effect of altitude on cooling-air requirement. Army air; velocity effect on temperature neglected; constant power.



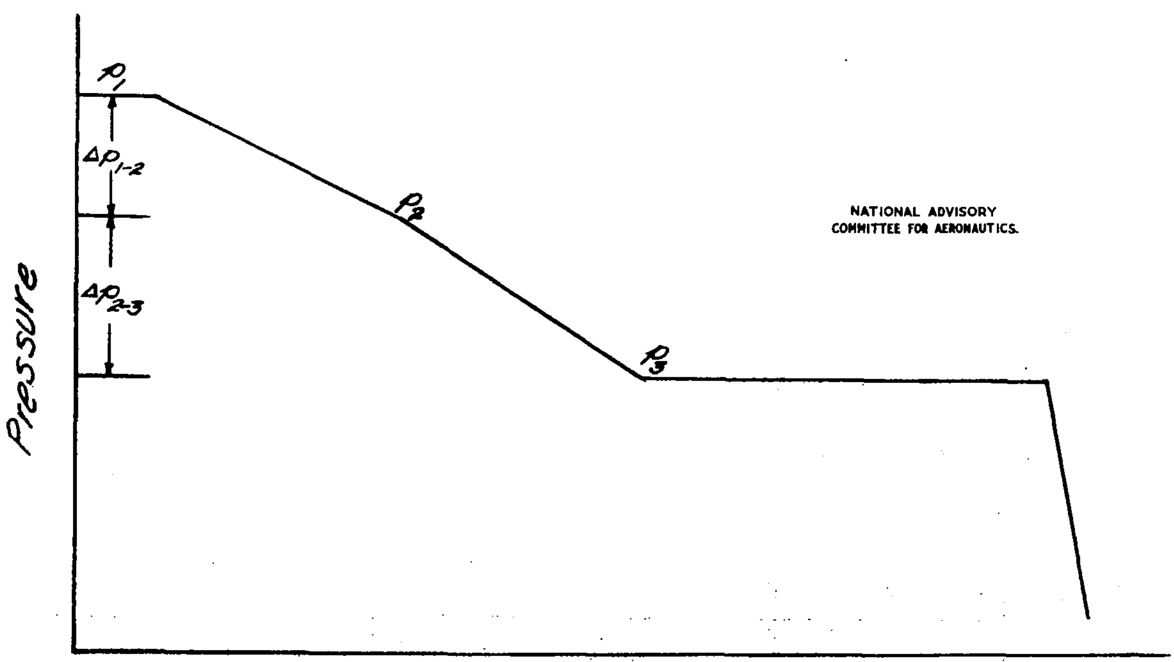
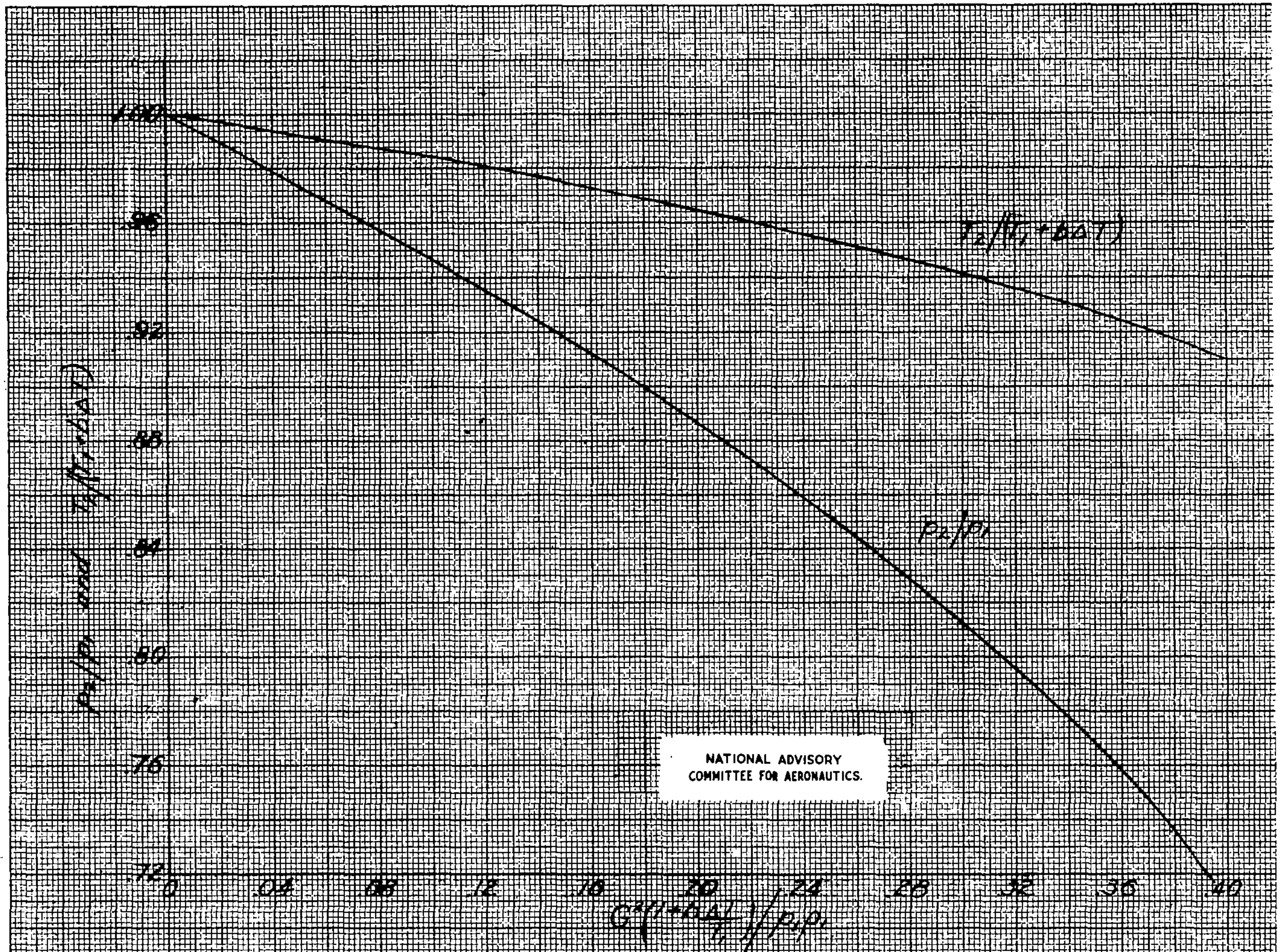
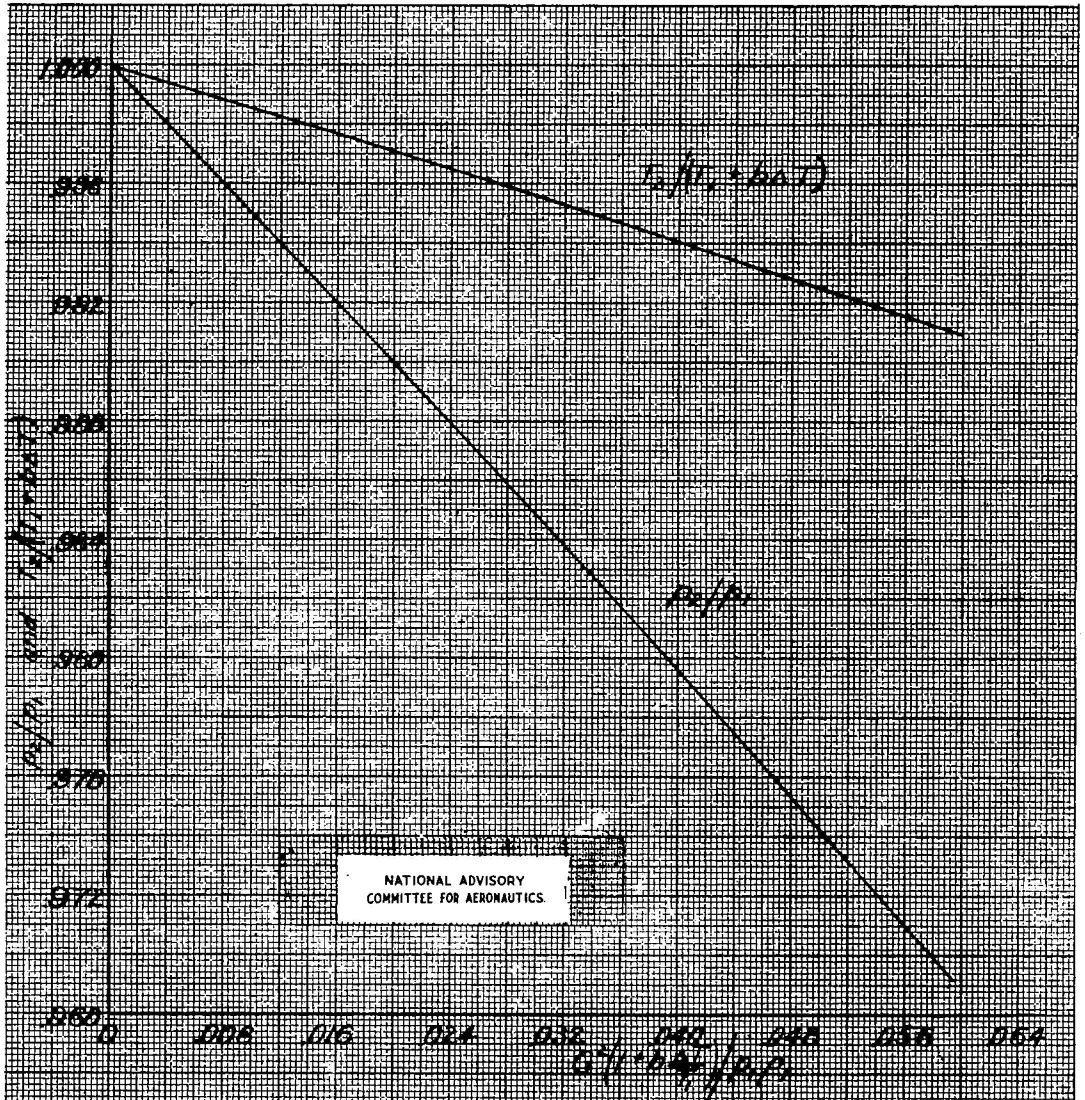


Figure 4.- Diagram of cooling-air flow path and pressure drops.



(a) Small values of p_2/p_1.

Figure 5.- Curve for determination of p_2/p_1 and $T_2/(T_1+b\Delta T)$.



(b) Large values of p_2/p_1.

Figure 5.- Concluded.

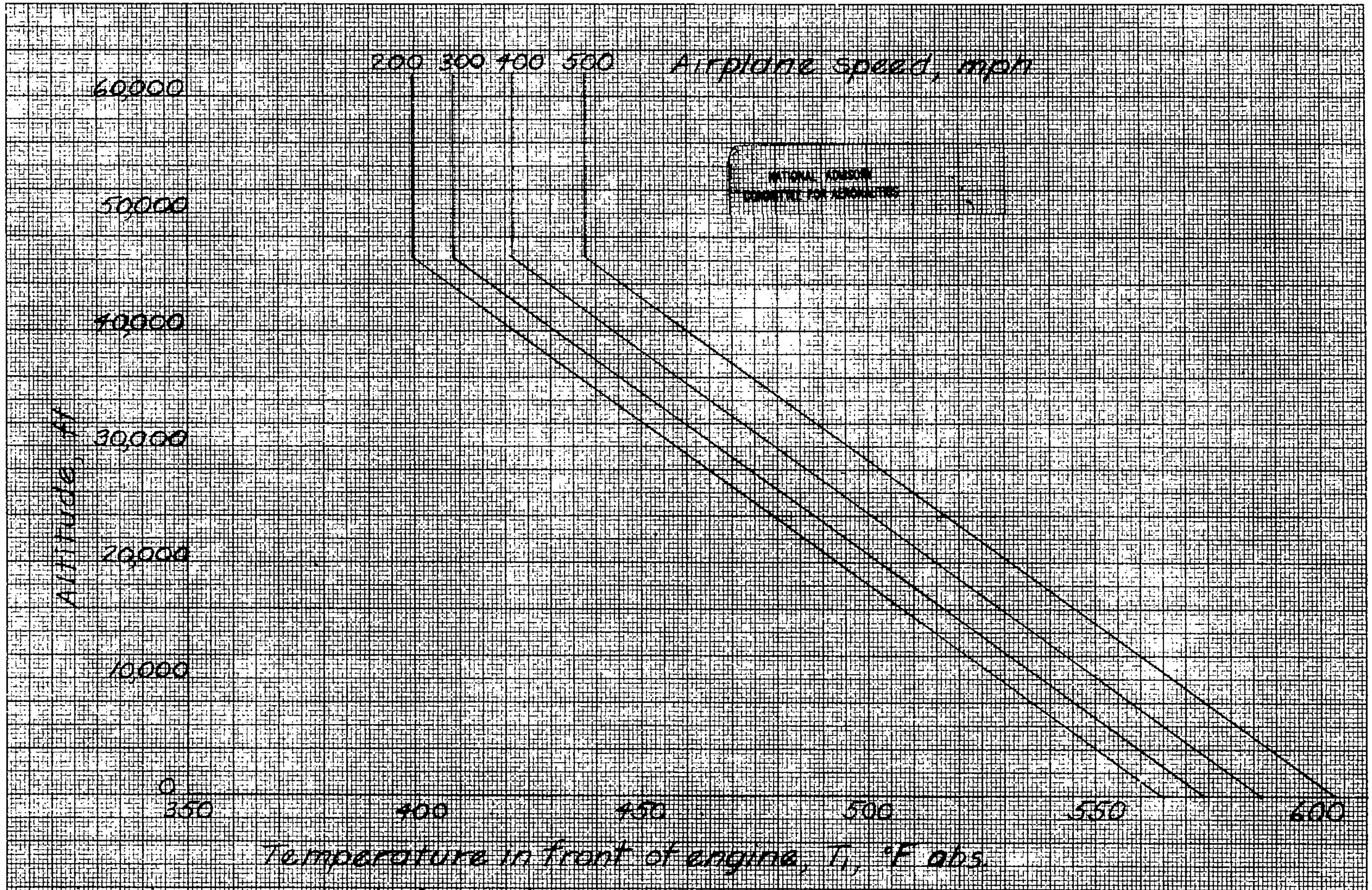


Figure 6.- Effect of altitude on stagnation temperature before engine, Army air.

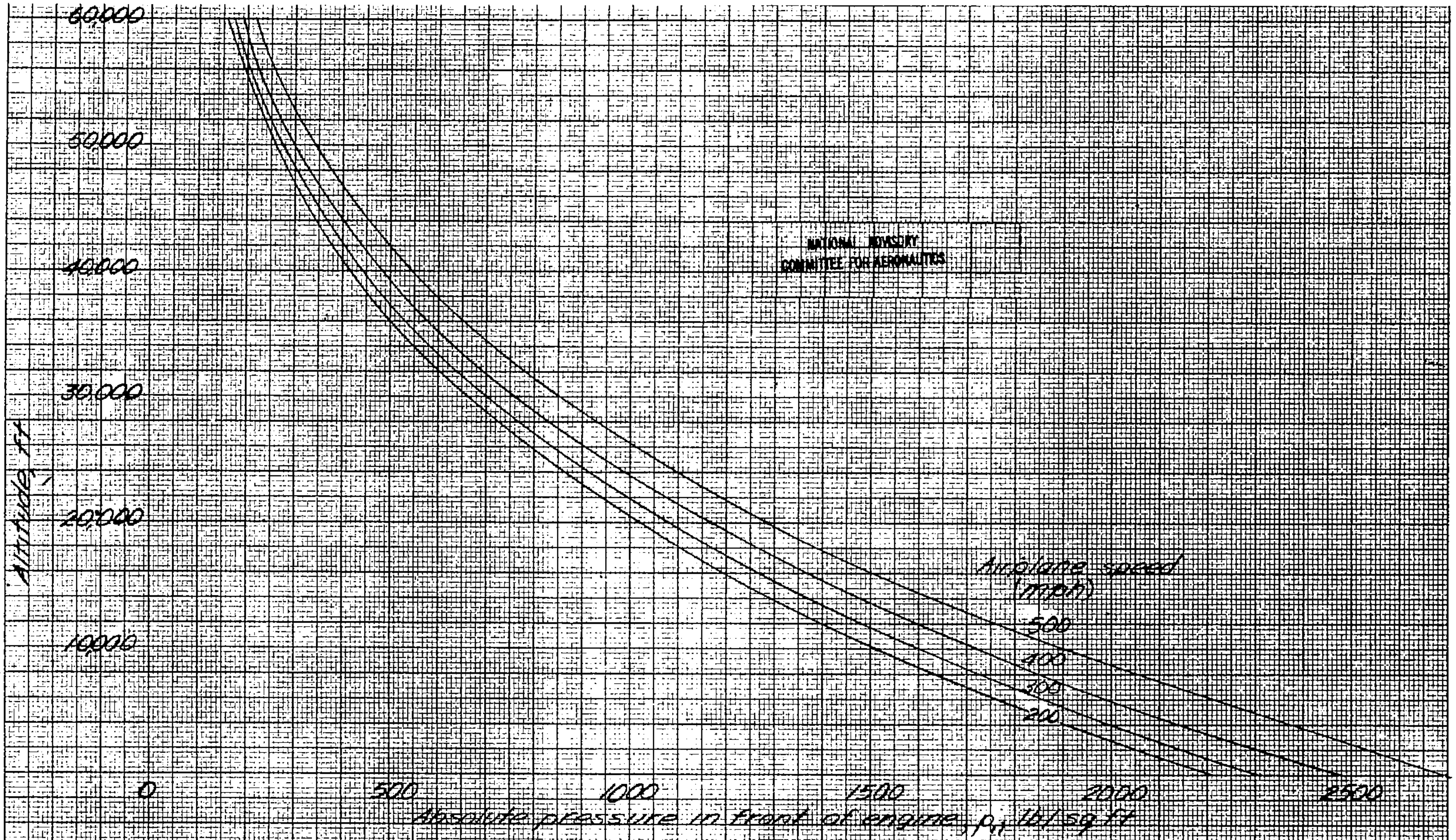


Figure 7.- Effect of altitude on stagnation pressure p_1 in front of engine. Army air; 90 percent ram.

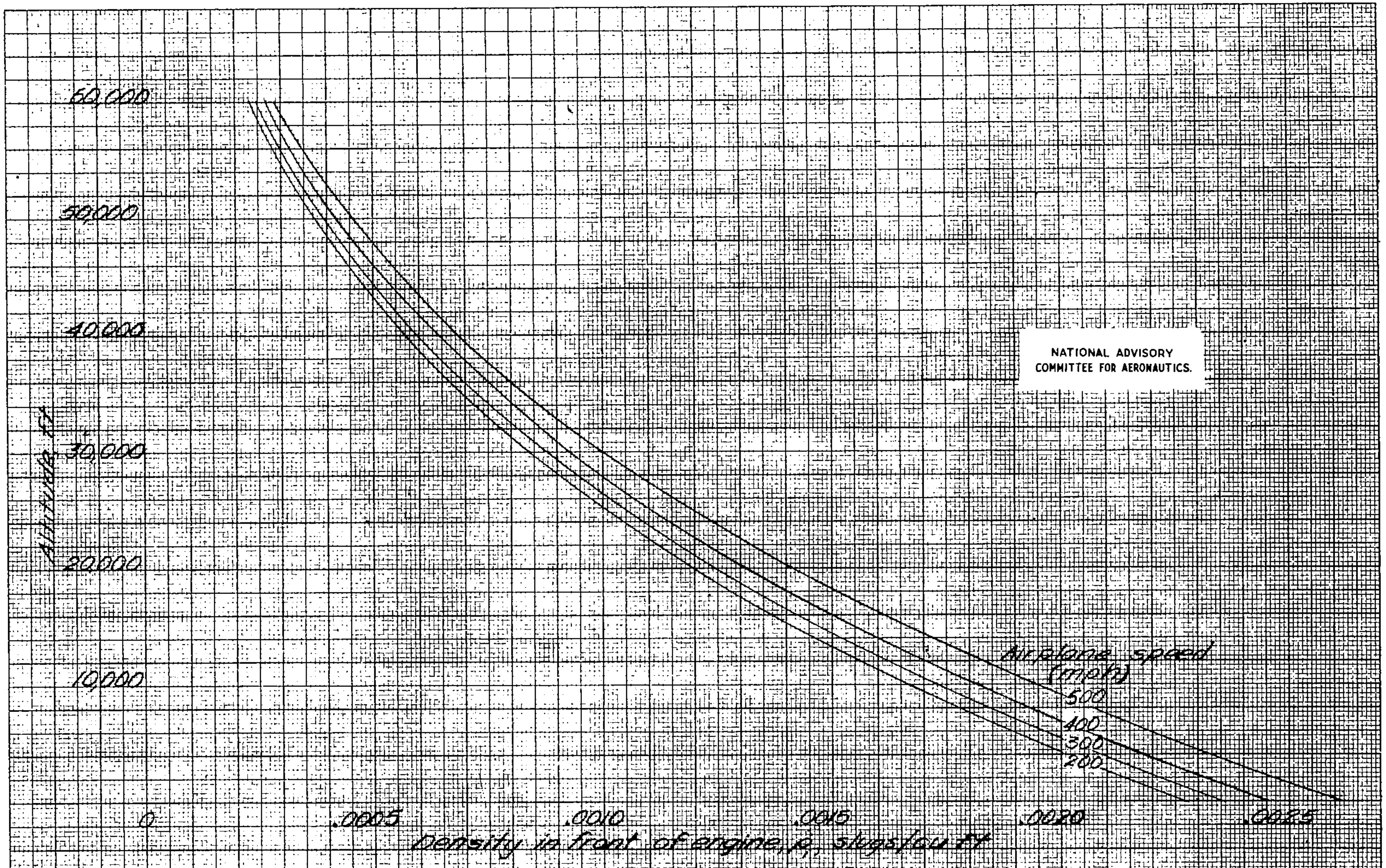
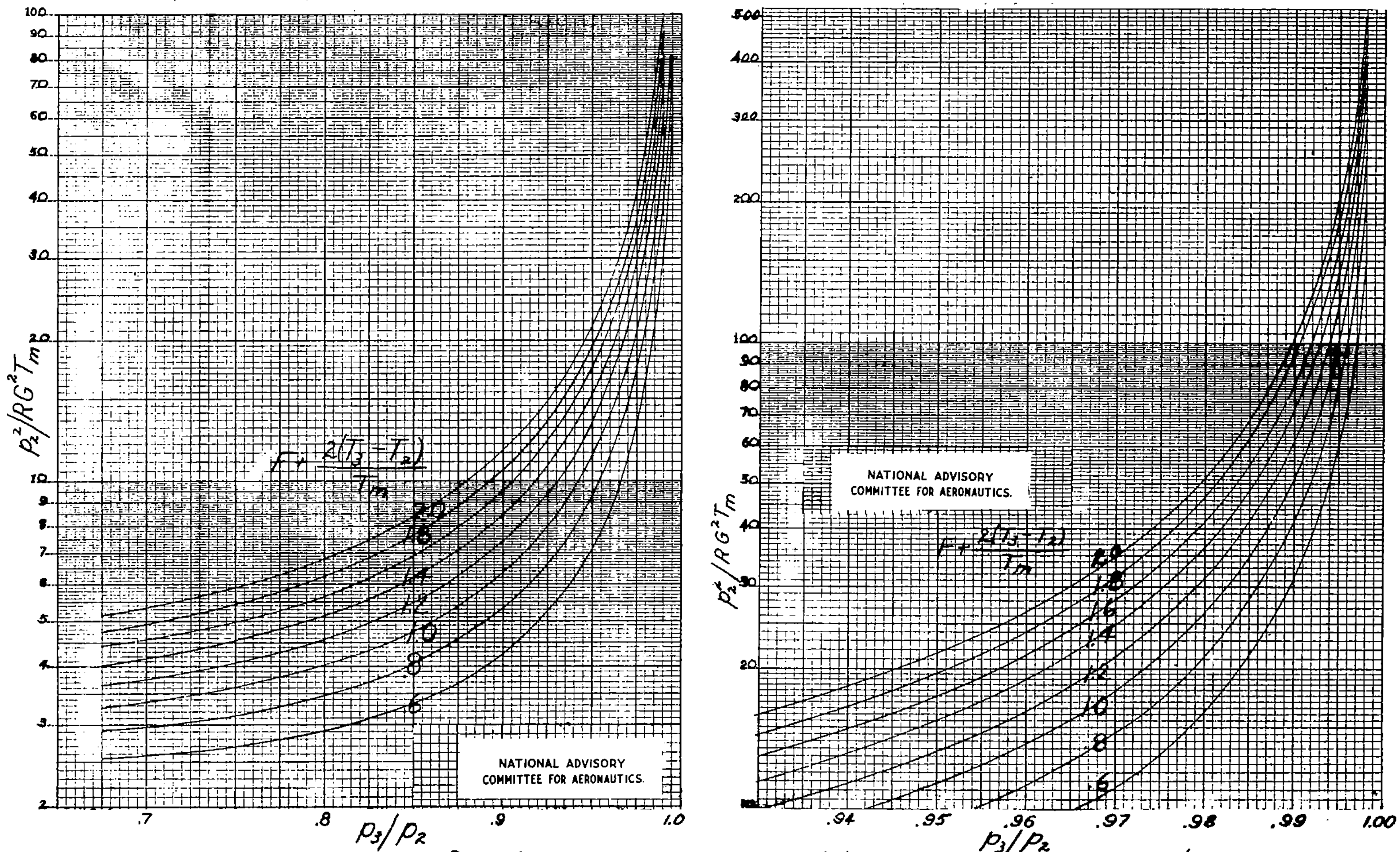


Figure 8.- Effect of altitude on stagnation density ρ in front of engine. Army air; 90 percent ram.



(a) Small values of p_3/p_2.

(b) Large values of p_3/p_2.

Figure 9.— Curves for determination of p_3/p_2.

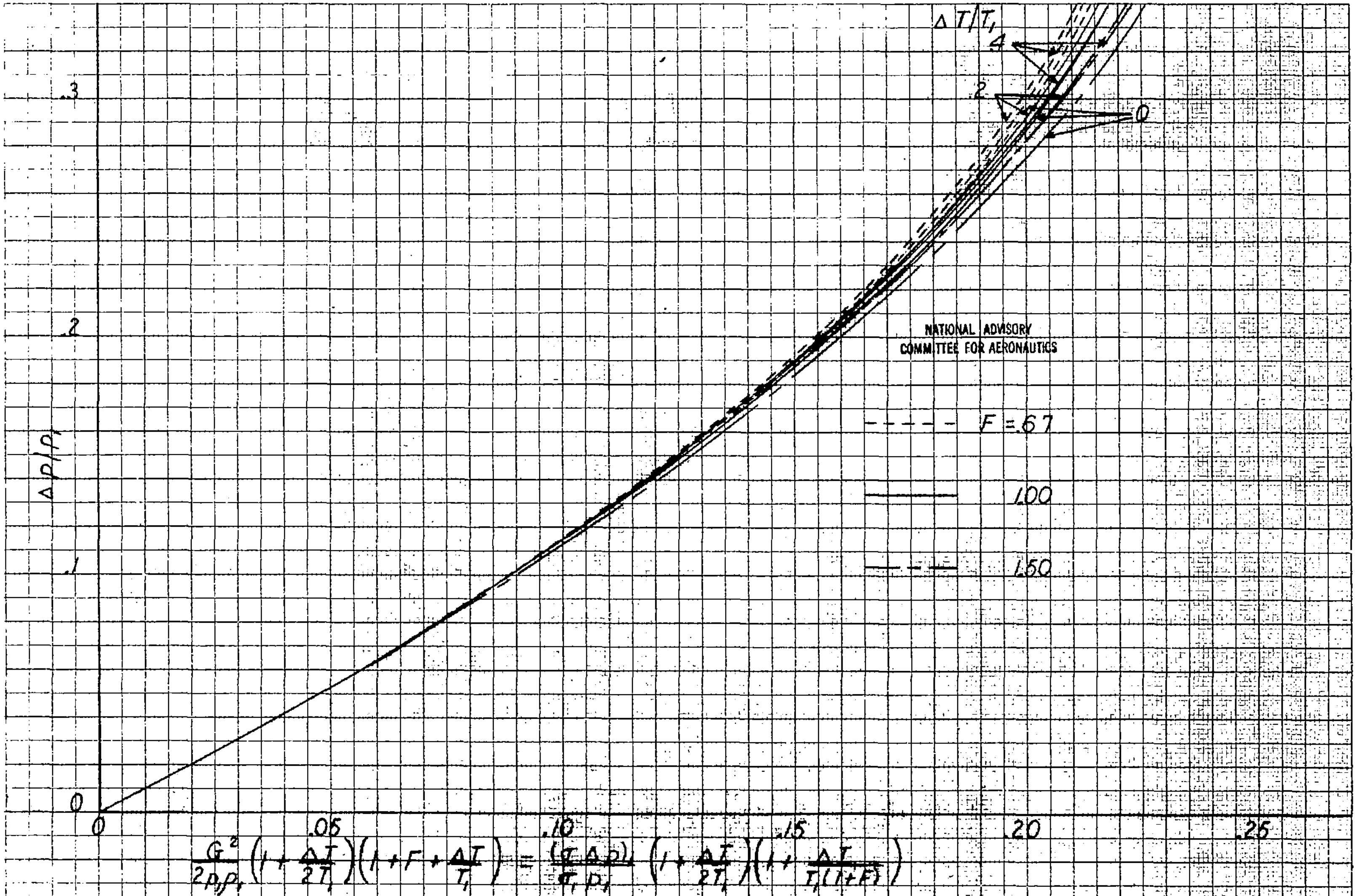


Figure 10.- Curves for finding $\Delta p/p_1$ from $(\sigma\Delta p)_1/\sigma_1 p_1$, $\Delta T/T_1$, and F.

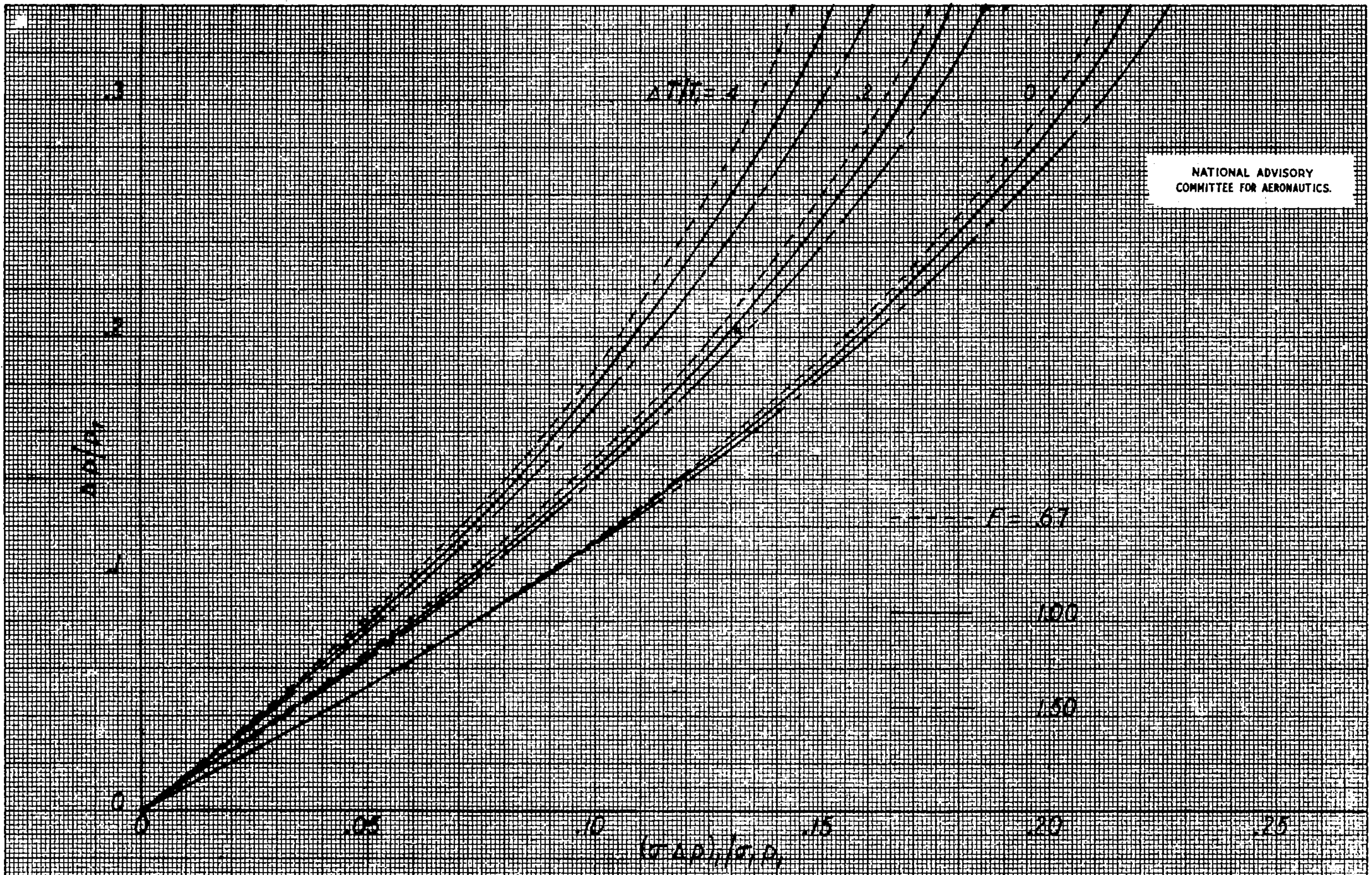


Figure 11.- Alternative curves of $\Delta p/p_1$ as a function of $(\sigma_1 \Delta p)_1/\sigma_1 p_1$, $\Delta T/T_1$, and F.

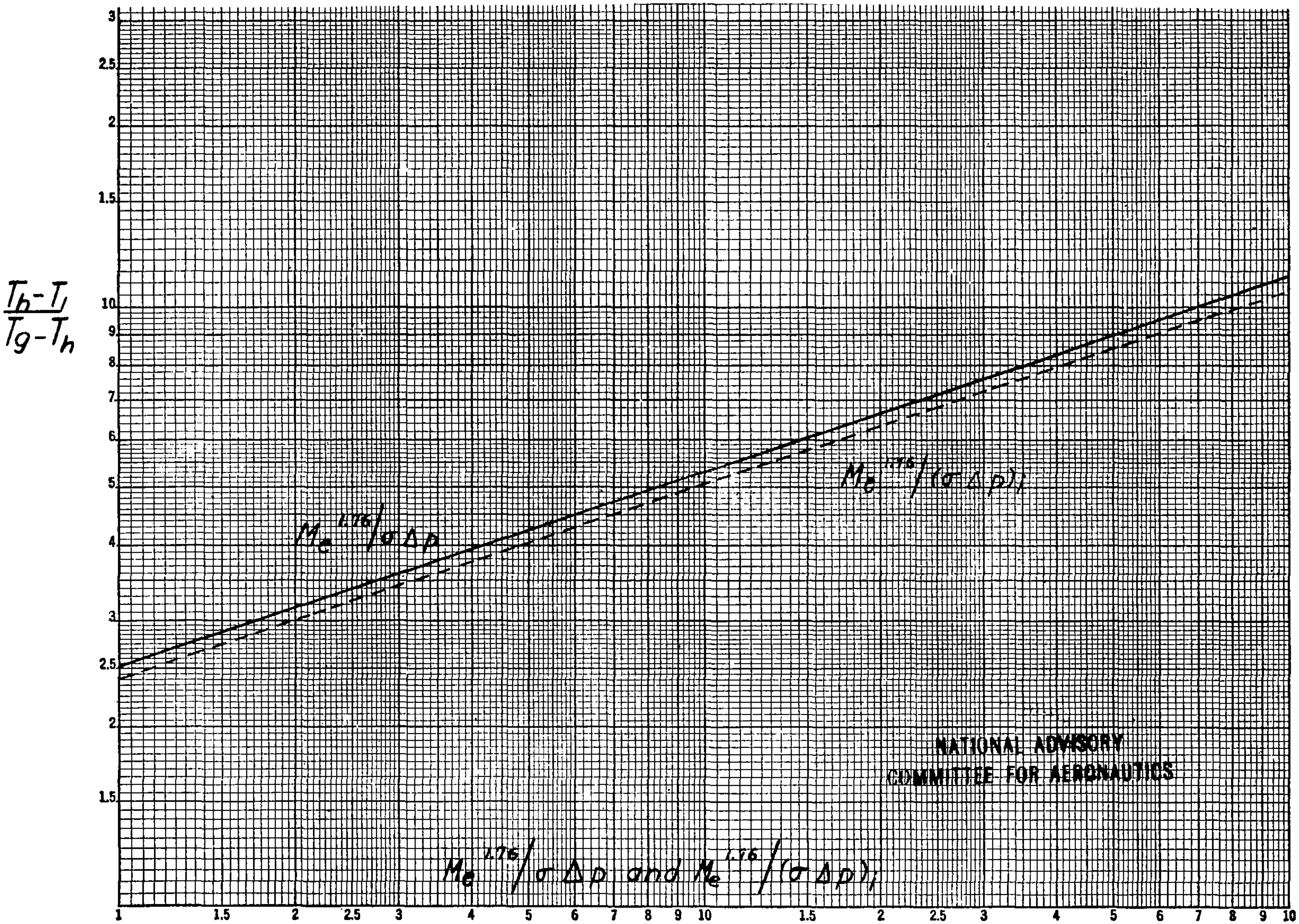


Figure 12.- Cooling correlation curve for the example engine.

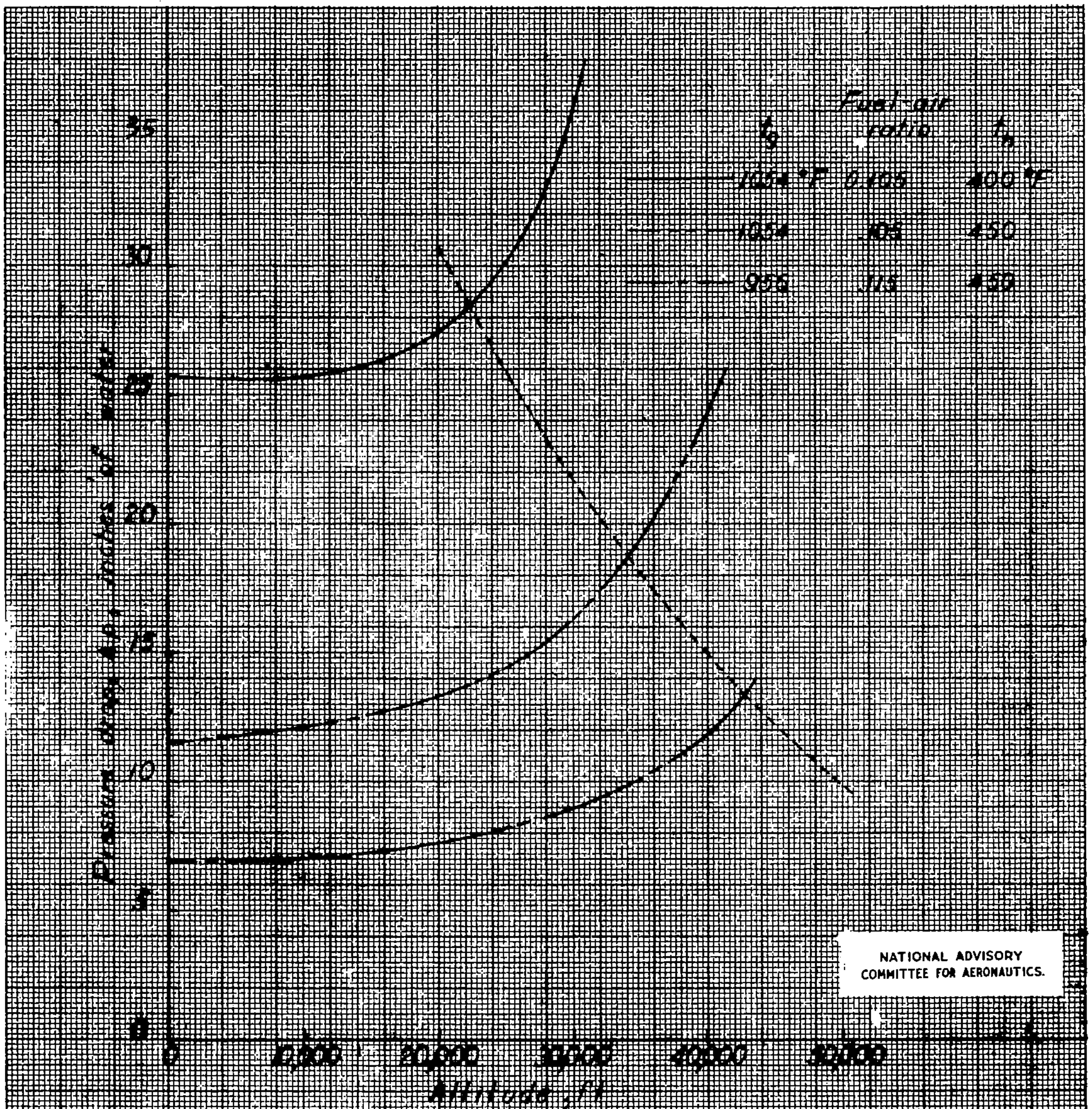


Figure 13.- Effect of altitude on the required cooling pressure drop for the example engine of 2000 hp; Army air; airplane speed, 350 mph. Dotted line shows dynamic pressure at 350 mph.